Developing a validated hydrocode model for adobe under impact loading

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Highlights
- We develop a validated hydrocode model for impact loading of adobe.
- In this model, strength and failure depend on pressure, Lode angle, and strain rate.
- Employed material parameters originate from test data and engineering assumptions.
- Including porosity in the equation of state is found to be crucial.
- Ballistic experiments are quantitatively reproducible with a unified parameter set.

Abstract
We present hydrocode simulations of penetration and perforation of adobe targets by high-hardness spherical steel projectiles. For the description of adobe we employ the RHT concrete model, which describes strength and failure depending on pressure, Lode angle, and strain rate in combination with a porous equation of state. In order to apply this model to adobe we develop a parameter set partially based on material test data. Other parameters are obtained through engineering assumptions, and their importance is assessed by sensitivity analyses. Ballistic reference experiments are reproduced well by our simulations especially regarding residual velocities of projectiles perforating finite-thickness targets. Therein, we also properly capture the deformation of the projectile and the damage of the target. Simulations of the penetration into semi-infinite targets are in acceptable agreement with the ballistic data. However, a significant influence of target setup and boundary conditions on the resulting penetration depths is discovered. The residual velocities obtained by perforation simulations do not show such sensitivity but considerably depend on the yielding threshold of the projectile. Hence, the latter property of the projectile must be correctly included in the simulation in order to be able to draw conclusions on the ability of the material model and the model parameters to describe the target. As for other building and geological materials, the pore compaction is revealed to be an important form of energy dissipation also for adobe and is hence an essential ingredient for the description of the ballistic performance of this material.

Keywords: Adobe, constitutive model, hydrocode simulation, impact, RHT concrete model

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1. Introduction

Modeling the dynamic loading and response behavior of building materials is of interest for diverse applications, especially in the contexts of impact, penetration, and blast. As highly developed protective structures are often made from concrete, a vast majority of related scientific publications is considering this class of material, e.g. Refs. [1-10]. Less frequently, other building materials were investigated in a similar way [11-13]. Related to such investigations are studies with geological materials [14] among which sand has been of great interest for different researchers [15-18].

In the present paper we aim at characterizing and modeling the impact response of adobe, which is a building material of low strength and relatively low density. Nonetheless it is of relevance for recent applications. Besides studies on material characterization [19-21], a couple of publications have addressed projectile penetration processed into adobe targets [22-26]. However, such investigations are predominantly experimental. In contrast, examples of numerical modeling of impact and penetration of adobe are yet scarce [27, 28] and are based on a first-generation concrete model [29, 30].

In order to push this field forward, we develop a validated hydrocode model that fully covers the phenomenology of the projectile-target interaction. As a starting point for further research, we choose the RHT concrete model [6-10]. Its basic features are reviewed in the next section. After that, an initial material parameter set for modeling adobe with the RHT model is derived based on publicly available material data [19, 20, 30] and engineering assumptions. In the following, these parameters are utilized for numerical simulations of the penetration and perforation of adobe targets by spherical steel projectiles. Validation of the simulation results is achieved through a comparison to reference experiments [22, 25, 26] covering a range of impact velocities. Thereby, residual velocities, penetration depths, damage patterns of the target and deformation of the projectile are considered in order to determine one consistent set of material parameters that is able to reproduce all experimental data quantitatively as well as the phenomenology. We further investigate the sensitivity of the perforation simulation results on several estimated material parameters of projectile and target. Additionally, a mesh convergence study is performed. Besides these validation steps, we also examine the influence of target setup and boundary conditions on the target damage and penetration depths for the deep penetration simulations.

2. Material models

The simulations presented in this work are performed with a hydrocode. This class of numerical tools (here the commercial software ANSYS-AUTODYN-V15.0 [31]) solves a set of partial differential equations derived from the conservation of mass, momentum, and energy together with an equation of state and a constitutive model [32-34]. The latter two are specific for a particular material and should take into account all the key physical processes (mechanics, thermodynamics etc.) relevant for the simulated process. Hereby, the equation of state relates pressure, density, and temperature of the involved materials while the constitutive equation takes care of their response to deviatoric mechanical loading. The latter is relating stresses and strains as well as defining the materials behavior in the elastic and plastic regimes. Furthermore, the onset of material failure is included in the constitutive model. Details on hydrocodes including an overview of the different methods (Lagrange, Euler, etc.), numerical solution schemes (finite difference and finite element) and time integration can be found in Refs. [32-34].

In the following, explicit time integration is performed in the simulation of the impact of two structured Lagrange bodies. Hence, the coordinate systems are fixed at the interacting
masses and move with them while the partial differential equations are solved with a finite difference scheme for each calculation cycle. However, before presenting the simulation results in section 3, the employed material models are summarized in this section. First, the RHT concrete model [6-10], later used for modeling the adobe target, is discussed in a general way. Then the determination of the model parameters for the description of adobe developed here is presented. Finally, in the last subsection, the material model used for the steel projectile is explained.

2.1 A short review of the RHT concrete model

The following subsection is divided into three parts: the equation of state, a constitutive model of strength, and an evolution law for the onset and propagation of material damage. It thus provides a brief overview of the RHT-model, for details see Refs. [6-10].

2.1.1 Equation of state

![Pressure vs. Density](image)

For the fully compacted (pore free) material, an equation of state of the Mie-Grüneisen form (Eq. (1a)) is used to relate the pressure $p$ to the density and the internal energy $e$. In compression ($\eta>0$), it is parametrized as a third-order polynomial with parameters $A_1$, $A_2$, and $A_3$, while in extension ($\eta<0$), only a linear dependence on the compression $\eta$ with the same parameter $A_1$ is used (see also Table 1 and Ref. [31]). In addition, Eq. (1b) takes care of the path to full compaction by introducing a porosity parameter $\alpha$ [35].
\[
p(\rho, e) = A_1\eta + A_2\eta^2 + A_3\eta^3 + \Gamma \rho e \quad \text{with} \quad \eta = \frac{\rho}{\rho_0} - 1
\] (1a)

\[
p = f(\rho_{\text{matrix}}, e) \rightarrow p = f(\rho, e) \quad \text{with} \quad \alpha = 1 + (a_{\text{init}} - 1) \left[ \frac{P_{\text{comp}} - p}{P_{\text{comp}} - P_{el}} \right]^N
\] (1b)

Hereby, \( \Gamma \) is the Grüneneisen parameter, \( \rho \) the current density, and \( \rho_0 \) the initial density. By the introduction of the porosity \( \alpha = \rho_{\text{matrix}}/\rho_{\text{porous}} \) as an additional state variable (Eq. (1b)), the compaction of the pores in the material and the compaction work that comes along with this permanent deformation can be included in the equation of state [35]. Hence, the pore compaction path is determined by the initial porosity \( \alpha_{\text{init}} \), the compaction exponent \( N \), and two limiting pressure values, i.e. the initial compaction pressure \( p_{el} \) and the solid compaction pressure \( p_{\text{comp}} \). These parameters determine under which pressure pores start to collapse and when they are fully compacted, respectively.

Fig. 1 illustrates this pore compaction path in a \( p \) vs. \( \rho \) plot. After the initial compaction pressure \( p_{el} \) is exceeded, the pores collapse according to the path (blue curves) defined by the compaction exponent \( N \) and the solid compaction pressure \( p_{\text{comp}} \). At higher pressures, pores are fully compacted and the density changes in a different relation to the pressure (solid green curve) than for the pore free matrix material (dashed green curve). This deviation originates from the additional energy transferred into the matrix material through the pore compaction process. Hence different pressure-density-energy-relation emerge from the pressure-density-energy-surface for these energetically different compaction cases.

2.1.2 Limit surfaces

In the RHT concrete model [6-10], the yield strength, the onset of failure, and the residual strength (after complete failure) are defined by three limit surfaces in stress space [36]. For a schematic picture of these limit surfaces see for example Fig. 2 in Ref. [8]. Hereby, it is the failure surface, \( Y_{\text{fail}} \) in Eq. (2), from which the other two surfaces are derived since the former can be directly measured on hydraulic machines with triaxial control.

\[
f(p, \sigma_{eq}, \theta, \dot{\varepsilon}) = \sigma_{eq} - Y_{\text{fail}}(p, \theta, \dot{\varepsilon}) = \sigma_{eq} - Y_{\text{TXC}}(p) R_3(\theta) F_{\text{Rate}}(\dot{\varepsilon}) = 0
\] (2)

\[
Y_{\text{TXC}}^* = A \cdot \left( p^* - HTL^* \right)^n
\] (3)

Eq. (2) shows that the failure surface \( Y_{\text{fail}} \) depends on the pressure \( p \), the Lode angle \( \theta \), and the strain rate \( \dot{\varepsilon} \). The pressure dependence as a function of the normalized pressure \( p^* = p/f_c \) for \( p^* \geq 1/3 \) is described by Eq. (3) with the intact failure surface constant \( A \) and exponent \( n \). For \( p^* < HTL^* \), the function \( Y_{\text{TXC}}^* \) vanishes by definition. The Hugoniot tensile limit \( HTL^* \) hereby denotes the maximum value of normalized tensile stress the material can sustain at vanishing deviatoric stress, i.e. under pure hydrostatic tension. All values with an asterisk are normalized by the uniaxial compressive strength \( f_c \). The stress triaxiality dependence of \( Y_{\text{fail}} \) indicated by the Lode angle \( \theta \) dependence in Eq. (2) is explicitly given in Ref. [37]. The function \( R_3 \) with \( Q_2 \leq R_3(\theta) \leq 1 \) reduces the failure surface for shear and tensile loading depending on the parameter \( Q_2 \). In general, this parameter is described as a function of pressure. However, such a brittle to ductile transition is not included in our following description of adobe (see Fig. 2 and corresponding discussion in 2.2.1). Hence, \( Q_2 \) will be a constant in the utilized parameter set (see Table 1).

The strain rate enhancement of deviatoric strength is included in the failure surface \( Y_{\text{fail}} \) through the function \( F_{\text{Rate}}(\dot{\varepsilon}) \). According to Ref. [38], the empirical fit functions given in
Eqs. (4a,b) and (5a,b) are able to take this rate effect into account for the uniaxial strengths in compression and tension. However, in this work, only Eq. (4a) for hydrostatic pressures above $f/3$ and Eq. (5a) for those below $f/3$ are used. In between these limiting pressures, $F_{\text{Rate}}$ is linearly interpolated.

**Compression (4a,b)**

\[
F_{\text{Rate}} = \frac{f_{cd}}{f_c} = \left( \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right)^\alpha \quad \dot{\varepsilon} \leq 30s^{-1}
\]

\[
\frac{f_{cd}}{f_c} = \gamma \sqrt{\dot{\varepsilon}} \quad \dot{\varepsilon} > 30s^{-1}
\]

with $\dot{\varepsilon}_0 = 30 \cdot 10^{-6}s^{-1}, \alpha = \frac{1}{5 + \frac{4}{3} f_c}$, \quad \text{and} \quad \log \gamma = 6\alpha - 0.492

**Tension (5a,b)**

\[
F_{\text{Rate}} = \frac{f_{id}}{f_i} = \left( \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right)^\delta
\]

\[
\frac{f_{id}}{f_i} = \eta \sqrt{\dot{\varepsilon}}
\]

with $\dot{\varepsilon}_0 = 3 \cdot 10^{-6}s^{-1}, \delta = \frac{1}{10 + \frac{1}{2} f_c}$, \quad \text{and} \quad \log \eta = 7\delta - 0.492

Hence, the dynamic increase of deviatoric strength is determined by the compressive strain rate exponent $\alpha$ and the tensile strain rate exponent $\delta$, which both are a function of the uniaxial compressive strength $f_c$ within this framework.

With the above discussed failure surface $Y_{\text{fail}}$, the elastic surface $Y_{\text{elastic}}$ is constructed as shown in Eq. (6). In a first step, $Y_{\text{fail}}$ is hereby multiplied with the elastic scaling function $F_{\text{elastic}}$, which is equal to $f_{c,\text{eff}}/f_c$ for pressures below $f_{c,\text{eff}}/f_c$ and above $f_{c,\text{eff}}/f_c$, it takes the value $f_{c,\text{eff}}/f_c$. A linear interpolation with respect to pressure is performed in between these limiting pressures values. In a second step, the failure surface is cut off elliptically by the cap function $F_{\text{cap}}$. Setting the elastic deviatoric stress limit to 0 for a pressure above the initial compaction pressure $p_{\text{el}}$ (see Eq. (1b)) ensures consistency of the elastic limit surface with the pore compaction mechanism built into the equation of state.

\[
Y_{\text{elastic}} = Y_{\text{fail}} F_{\text{elastic}} F_{\text{cap}}
\]

\[
Y_{\text{hard}} = Y_{\text{elastic}} + \frac{\varepsilon_{eq}^{\text{pl,hard}}}{\varepsilon_{eq}^{\text{pl}}} (Y_{\text{fail}} - Y_{\text{elastic}})
\]

\[
\varepsilon_{eq}^{\text{pl,hard}} = \frac{(Y_{\text{fail}} - Y_{\text{elastic}}) (G_{\text{elastic}})}{3G}
\]

Work hardening is included in the RHT concrete model by an interpolation of the initial elastic surface $Y_{\text{elastic}}$ and the failure surface $Y_{\text{fail}}$, which is scaled by a ratio of equivalent plastic strains (Eq. (7a)). The plastic stiffness of this hardening surface interpolation factor is determined by the shear modulus ratio according to Eq. (7b). Consequently, plastic strain leads to a growth of $Y_{\text{elastic}}$ that then results in $Y_{\text{hard}}$. Furthermore, due to the strain rate dependence of $Y_{\text{fail}}$, a growth of $Y_{\text{elastic}}$ for higher strain rates is also included in the above presented model.

2.1.3 Damage evolution and failure

In addition to the failure threshold discussed above, the RHT model also describes the incremental evolution of failure beyond that threshold as well as the residual strength of damaged material. The evolution of failure incorporated in the RHT concrete model is based on the model by Johnson and Holmquist [29]. As presented in Eq. (8), the damage parameter $D$
increases incrementally with effective plastic strains occurring beyond the ultimate strength defined by the failure surface. These strains are normalized by the effective strain to failure $\varepsilon_{eq,pl,\text{fail}}$, which depends on pressure, and the two shape parameters $D_1$ and $D_2$ as depicted by Eq. (9). Towards lower pressures a minimum value $efmin$ is used for $\varepsilon_{eq,pl,\text{fail}}$.

$$D = \int_{\varepsilon_{eq,pl,bad}}^{\varepsilon_{eq,pl,sad}} \frac{1}{\varepsilon_{eq,pl,\text{fail}}(p)} d\varepsilon_{eff,\text{incremented}} = \sum \frac{\Delta \varepsilon_{eq,pl}^{\text{pl,\text{fail}}}}{\varepsilon_{eq,pl,\text{fail}}(p)}$$  \hspace{1cm} (8)

$$\varepsilon_{eq,pl,\text{fail}}(p^*) = D \cdot (p^* - HTL) \geq efmin$$  \hspace{1cm} (9)

$$Y_{\text{fric}} = B \cdot p^m, \quad Y_{\text{damaged}} = Y_{\text{fail}} + D(Y_{fric} - Y_{\text{fail}})$$  \hspace{1cm} (10a,b)

As damage accumulates $D$ increases ($0 \leq D \leq 1$) and the damage surface $Y_{\text{damaged}}$ increasingly moves towards the residual strength surface $Y_{\text{fric}}$ (see Eq. (10b)). In the case of complete damage ($D = 1$), the strength of the material is solely described by $Y_{\text{fric}}$ which means that no resistance to tensile loading remains. In this work it is assumed that the parameters of the residual strength surface $Y_{\text{fric}}$ are equal to the ones of the limit surface $Y^*_{TXC}$ in Eq. (3). Hence we set $B = A$ and $m = n$ (see Table 1).

2.2 Determination of model parameters for adobe

The parameter set for the hydrocode simulations modeling impact events onto adobe targets, called Adobe1.8-RHT, originates from three different sources of material characterization experiments. Then, the parameters retained from the original RHT concrete parameter set are discussed. After that, parameters based on assumptions by engineering judgement, which are later on varied in the sensitivity analyses, are explained.

2.2.1 Parameters derived from material test data

Material characterization experiments performed at EMI and in Ref. [19] are the source of some equation of state and most of the strength parameters summarized in Table 1. Although the adobe materials used in these two references do not exhibit exactly the same properties, we consider them as similar enough for using the test results of EMI and Ref. [19] for a common parameter set. The porous density of $\rho_{\text{porous}} = 1.77 \text{ g/cm}^3$, the longitudinal sound velocity (under uniaxial stress) of $c_l = 1050 \text{ m/s}$ (resulting in a porous bulk sound speed of $c_{b,\text{porous}} = 835 \text{ m/s}$), and the compressive strength of $f_c = 3.8 \text{ MPa}$ emerge from material testing at EMI. Moreover, Young’s Modulus ($E = 2.0 \text{ GPa}$) obtained from these longitudinal sound speed measurements together with the Poisson’s Ratio ($\nu = 0.23$) determined in Ref. [19] allow us to calculate the shear modulus. The ratio of the tensile and compressive strength ($f_{t}/f_c = 0.04$) also stems from Ref. [19].

Furthermore, in Ref. [19] results of triaxial compression (TXC) tests are given up to a confining pressure of $400 \text{ MPa}$ while triaxial extension (TXE) measurements with a maximum confining pressure of $60 \text{ MPa}$ are presented. Our evaluation of this data is presented in Fig. 2 and results in the intact failure surface constant $A$ and exponent $n$ (see Eq. (3)) and, with the assumption presented in 2.1.3, also in the fractured strength constant $B$ and exponent $m$ (see Eq. (10a)). By scaling the resulting TXC failure surface (solid blue curve) in order to match the continued TXE failure surface (thin solid red curve), we obtain the tensile/compressive meridian ratio $Q_2$ and discover that a brittle to ductile transition is not vital for matching both surfaces. Hence, we neglect this effect and set the corresponding parameter to $0$ (see Table 1).
Consequently, the failure surface of our hydrocode model for adobe used later on originates from triaxial test data and can hence be considered trustworthy. Thus, no sensitivity analysis of the failure surface parameters is performed. Furthermore, the damage constants $D_1$ and $D_2$ result from data in Ref. [19]. These two values can also be found in Ref. [30].

![Diagram](image)

**Fig. 2**: Determination of the failure surface parameters. The triaxial compression (TXC) and triaxial expansion (TXE) test data from Ref. [19] is fitted with Eq. (3) to extract the parameters $A$ and $n$. The parameter $Q_2$ is determined by matching the TXC fit curve (solid blue) with the extended TXE fit curve (thin solid red).

### 2.2.2 Parameters retained from the original concrete parameter set

The initial approach of utilizing the RHT concrete model [6-10] for the theoretical description of high rate loading of adobe is based on multiple similarities of adobe and concrete. Both of these materials, for example, exhibit a pore compaction upon compression and are able to resist a much higher stress in compression than in tension before the onset of plastic deformation or failure. Consequently, many of the model parameters used for example in Ref. [8] are retained for the material parameter set used in this work for adobe. Primarily all parameters of the solid equation of state and all erosion related parameters are unchanged with respect to the values in Ref. [8]. Additionally, the shear strength (in Table 1 over $f_c$) is determined by approximately keeping the ratio of tensile strength to shear strength of the RHT concrete model parameters in Ref. [8]. Moreover, all remaining strength parameters except for the two rate exponents and the elastic strength/$f_t$ are equal to this parameter set. So are the yet not mentioned parameters for the damage evolution law in Table 1. The selection of parameters which are retained from the parameter set describing concrete is based on the assumption of a qualitatively similar behavior of concrete and adobe.
2.2.3 Parameters based on assumptions by engineering judgement

Lacking detailed shock data, e.g. from plate impact tests, the values for the initial and solid compaction pressure and the compaction exponent (all in Eq. (1b)) are estimated. For that, we use our experience on different types of concrete and other geological materials. This engineering judgement is later on tested by sensitivity analyses, and the simulation results are discussed by comparing them to the ballistic data of Refs. [22, 25, 26] (see Fig. 8 and related discussion in 4.4). Yet, actual physical investigations by plate impact tests are highly recommended. The elastic strength/f; on the other hand was estimated to be lower than for concrete since the ratio of f/f; is also considerably lower (adobe: 0.04, concrete: 0.10). In contrast, the compressive (α) and tensile (δ) strain rate exponents are obtained with the measured value of compressive strength inserted into the formulas given in Eqs. (4) and (5). Whether these empirical formulas are also applicable for adobe cannot be argued at this point. Hence, the rate exponents are later on varied in a sensitivity analysis (see Fig. 9 and related discussion in 4.5). Again, an assessment of their importance and a preliminary choice is made through the comparison with the results of existing ballistic reference experiments [22, 25, 26].

2.3 Material model and parameters for the steel sphere

The spherical projectiles used in the ballistic penetration and perforation experiments in Refs. [22, 25, 26] are bearing balls which consist of a high strength steel for which hardness measurements obtain a value of 820HV20. As high hardness is correlated with high yield strength, we choose the S-7 tool steel from Ref. [39], retain the values of the Johnson-Cook parameters for strain (B, n), strain rate hardening (C), and thermal softening (m), and adjust the yield stress parameter A in Eq. (11) to reproduce the measured deformation of the projectile (see Fig. 6 and corresponding discussion in 4.2). The resulting BearingSteel2.7YS parameter set is presented in Table 1. This material utilizes a shock equation of state, and the strength of this material is described by the Johnson-Cook constitutive model [39]

\[
\sigma_y = \left( A + B\varepsilon_p^n \right) \cdot \left( 1 + C \ln \dot{\varepsilon}_p^* \right) \cdot \left( 1 - T_H^* \right) [11]
\]

with the effective plastic strain ε_p, the normalized effective plastic strain rate \( \dot{\varepsilon}_p^* \), the homologous temperature \( T_H^* \), and the constants A, B, C, n, and m. No failure is described here since only moderate deformations are observed in the experiments which are simulated in this work.

3. Reference experiments

The presentation and analysis of the simulation results in the next section builds on data from impact experiments that are shortly reviewed in the following. For the purpose of model development and validation, impact experiments with spherical steel projectiles against adobe targets have been performed earlier [22, 25, 26]. The specific adobe material used in the experiments was characterized by a density of \( \rho_{porous} = 1.77 \text{ g/cm}^3 \), a compressive strength of \( f_c = 3.8 \text{ N/mm}^2 \), and a longitudinal sound velocity (under uniaxial stress) of \( c_l = 1,050 \text{ m/s} \). It is the same adobe material as the one characterized in the material tests at EMI (see 2.2.1).

Adobe bricks of nominal dimensions of 240 x 115 x 71 mm³ were used to build finite-thickness and semi-infinite laboratory targets. For this purpose, a single or multiple bricks in a row were embedded in a steel frame for lateral confinement. In case of the semi-infinite targets, there was also a steel confinement on the rear side, see Fig. 3. As will be shown below,
considering those target properties is important for capturing all occurring phenomena, especially those related to target damage.

![Schematic representation of target setup used in the reference experiments. Note that the adobe material consists of separate bricks with small spacings that are replicated in most of the simulations. This Figure also appeared in Ref. [26].](image)

In total, 21 experiments with 13.5 mm diameter steel spheres are presented in Refs. [22, 25, 26]. This data set comprises ten experiments with a semi-infinite target, eight experiments with 71 mm (one adobe brick) and three experiments with 142 mm (two adobe bricks) finite-thickness targets, respectively. The impact velocities range from 240 m/s up to 2050 m/s and cover the full regime of rigid-body penetration up to the onset of projectile plasticity and fragmentation at the upper end of the velocity range. Further details are given in the respective papers [22, 25, 26]. For the validation of the hydrocode model for adobe, only the subset of experiments without projectile fragmentation is used within the scope of the present paper.

4. Simulation Results and Discussion

The hydrocode simulations presented in this section are performed with the AUTODYN [31] default settings for the interaction of two structured Lagrangian parts. Therein, the material models presented in section 2 are employed with the parameter sets summarized in Table 1. In the following, these two sets of parameters describing the adobe target and the spherical steel projectile are called Adobe1.8-RHT and BearingSteel2.7YS, respectively. They will be referred to as the benchmark material parameters. For all penetration and perforation simulations, Coulomb friction with a constant of 0.15 between the steel sphere and the adobe bricks and a value of 0.3 between two adobe bricks is utilized. The estimated values of the static friction parameters are based on sensitivity studies performed in Ref. [9]. Therein static friction coefficients between steel and concrete are varied in penetration simulations. The extent of the influence of dynamic friction in penetration and perforation simulations is not fully understood at this point and we leave further studies of any friction related effects for future work. As an erosion criterion, instantaneous geometric strain is used for all materials. For adobe bricks, a cell is deleted at a geometric strain of 2 while for the steel sphere this would happen at a value of 1.5. However, in none of the simulations any of the cells of the sphere reaches this value. Our choice for the erosion criterion for adobe stems from a convergence study performed for concrete in Ref. [9] where a high enough value for the erosion strain is chosen so that there is no influence on the simulation result. For adobe this is further tested by a parameter study summarized in Table 4. The bearing steel sphere has a diameter of 13.5 mm and impacted adobe bricks have dimensions of 240 x 115 x 71 mm³ (height, width, thickness), as in the experiments mentioned in the previous section. In this section, first the perforation simulations with these configurations are presented in comparison to the available ballistic data [22, 25, 26]. Then, the influence of projectile strength and mesh resolution on simulation results is investigated. After
that, sensitivity studies of the pore compaction path parameters and the strain rate dependence of the strength of adobe are performed since both have been based on engineering judgement so far. Finally, depth-of-penetration simulations with semi-infinite targets are discussed with respect to the available experimental results [22, 25, 26].

4.1 Perforation of Adobe Bricks

![Fig. 4: Damage patterns and z-velocity evolution for an exemplary perforation simulation of one adobe brick with an initial velocity of 620 m/s. The mesh resolution of the target is 13.5 cells per diameter of the sphere (c/d). In comparison to the simulated final damage pattern, a picture of the adobe target after the experiment is displayed.](image)

A sequence of snap-shots from an exemplary hydrocode simulation of the perforation of an adobe brick by a spherical steel projectile is displayed in the top panel of Fig. 4. There it is presented how material damage develops with time which enables us to correlate the evolution of damage with the deceleration of the projectile. For these perforation simulations no boundary conditions are applied to the adobe bricks. The simulated penetration velocity as a function of time (Fig. 4, lower left) is obviously not linear, which indicates that the deceleration of the projectile is not constant. At first, undamaged material is penetrated so that we may consider conventional semi-infinite penetration roughly until time point (2). This first phase comprises an entry process (crater formation) and subsequent penetration (tunneling). Until point (2) there are no effects of finite target thickness that influence the projectile. At point (2) spall failure at the back surface of the adobe brick occurs due to the reflection of stress waves (spalling). As a result, there is a decreased deceleration rate of the projectile due to a breakdown of the penetration resistance of the target. This stems from the fact that the penetrated material is already fully damaged (see time point (3)) and from the ability of the target material to move towards the free surface so that it does not have to be transported around the projectile towards the edge of the penetration crater (shear plug formation/ejection).
Performing hydrocode simulations is a powerful method to capture the involved complicated mechanics of such a process, i.e. wave propagation and material response way beyond the elastic regime.

A comparison of the simulated damage pattern with the experiment is presented in the lower right panel of Fig. 4. Here it is revealed that the simulated final damage pattern correlates well with the observable extent of damage of the target in the experiment. Hence, our simulations are capable of successfully reproducing an important aspect of the perforation of an adobe brick by the RHT concrete model with the parameter set Adobe1.8-RHT.

In Fig. 5 the residual velocities of perforation simulations with one and two adobe bricks are plotted vs. the selected initial velocities. The values obtained by ballistic experiments (black symbols) are very well reproduced by the hydrocode simulations over a wide velocity range. Merely the simulated residual velocity for one brick with the highest initial velocity exhibits a significant deviation from the data.

The deformation of the projectile can also be compared to experimental data. The insets of Fig. 5 display a flash X-ray image of the (residual) steel sphere projectile after perforating the target together with the corresponding simulated shape for two different initial velocities. The fact that the same red dashed circle matches the shape of both experiment and simulation for different initial velocities shows that the (small) deformation of the projectile is correctly described by our simulation. The energy dissipation through plastic work corresponding to this deformation will be discussed in 4.2.

From the reproduction of the full phenomenology (damage pattern of the target, residual velocity, deformation of the projectile) we can conclude that the two sets of parameters denoted Adobe1.8-RHT and BearingSteel2.7YS (see Table 1) describe the target’s and the projectile’s
response to the impact with one another in these particular geometries well. Based on the good quantitative reproduction of the experimental data we can further deduce that the hydrocode model for adobe used in our simulations should generally be able to describe the response of adobe to similarly intense and highly dynamic loading cases reasonably well.

4.2 Sensitivity study on projectile strength

Returning to the topic of energy dissipation by plastic deformation of the projectile sphere, it is Fig. 6 which demonstrates the importance of appropriately including this aspect in the material model of the projectile. Here the influence of the variation of the yield stress constant $A$ in Eq. (11) on the simulated residual velocities of the perforation of a single adobe brick is displayed. The yield stress parameter $A$ (see Table 1) is the flow stress at vanishing effective plastic strain, the reference strain rate $\dot{\varepsilon}$, and room temperature. Decreasing the value of the yield stress constant $A$ leads to an increase in plastic work done on the projectile which reduces its residual velocity. From Fig. 6 it becomes clear that especially for the upper range of initial velocities ($>1200 \text{ m/s}$) the appropriate onset of plastic deformation of the sphere is crucial for the reproduction of the experimentally determined residual velocities. The inset demonstrates that a hardly visible deformation of the sphere (middle vs. right) leads to a noticeable change in the residual velocity. Consequently, this important mechanism of energy dissipation can easily be underestimated, which can prevent a proper judgement on the ability of certain parameters of the material model to properly describe the target.

![Fig. 6: Sensitivity analysis of projectile yield stresses $A$ (see Eq. (11)). Residual velocities as a function of initial velocities for perforation experiments and simulations with one adobe brick as a target. The inset shows simulated final shapes of the projectile for selected values of $A$. The identical red circles are a guide to the eye. The mesh resolution of the target is 13.5 cells per diameter of the sphere ($c/d$).](image-url)
The previously mentioned hardness measurement result of 820HV20 for the projectile suggests that the selected value of $A = 2.7$ GPa lies within an appropriate magnitude range. A direct and exact conversion, however, is difficult. Considering the actual plastic work on the sphere should be the appropriate quantity to discuss the importance of plastic deformation and hence the onset of the corresponding phenomenological transition. In Table 2, the plastic work on the sphere as a fraction of the total kinetic energy loss is given. Looking at these values and thus the fraction of energy dissipation by plastic work on the sphere, the benchmark simulations ($A = 2.7$ GPa) with initial velocities below 1000 m/s can be regarded as within the elastic regime. For the ones above 1500 m/s, the portion of plastic work of the total energy dissipation ($>1\%$) cannot be disregarded which is in agreement with the deformation of the sphere visible in the X-ray images (see Fig. 5). Benchmark simulations with initial velocities in between can be labeled as the region in which the onset of plasticity occurs. In contrast, in simulations with a yield stress parameter $A = 1.5$ GPa the onset of this phenomenological transition already occurs for initial velocities below 1300 m/s (see Table 2). At some magnitude of plastic deformation of the sphere, there will also be additional energy dissipation due to an increased penetration channel. Hence by vastly underestimating the flow stress of the projectile, the simulated residual velocity will be decreased by the additional dissipated energy corresponding to the deformation of the projectile and the increased interaction between projectile and target due to a blunter nose shape of the projectile.

4.3 Investigating mesh sensitivity

![Figure 7](image.png)

**Fig. 7**: Residual velocities as a function of initial velocities for perforation experiments and simulations with one adobe brick as a target for different mesh resolutions of the target, characterized by the cells per diameter of the sphere ($c/d$). The inset shows simulated residual velocities as a function of $c/d$ for the initial velocity of 930 m/s, a case of an almost entirely elastic behavior of the sphere.

Fig. 7 displays residual velocities versus initial velocities obtained in simulations with the benchmark material model parameters for different mesh resolutions of the target. Each of these meshes is graded, which means that the cell sizes gradually increase with increasing distance from the penetration channel. Within the penetration channel, however, the cubical cells do not exhibit a significant variation in size. Using a uniform mesh for the adobe brick
leads to a marginal difference in residual velocities (see Table 3). The number of cells across the radius of the sphere is adjusted to similar sizes as the ones of the target. The inset of Fig. 7 shows the residual velocities over the cells per diameter of the sphere (c/d) for a simulation in which the projectile is almost entirely in the elastic region. In this case a textbook convergence with the simulated values asymptotically approaching the experimental value is revealed. In the velocity range close to the onset of plastic deformation of the sphere, the situation is more complicated. A detailed analysis of the evolution of the residual velocities presented in Fig. 7 illustrates this issue. Therein it becomes clear that the mesh resolution of 13.5 c/d is the coarsest one to reproduce the measured residual velocity evolution, i.e. a linear increase with a proper slope. The coarser meshes fail to properly reproduce the data especially in the region of the higher investigated initial velocities where the onset of yielding of the projectile takes place. A comparison of the values of plastic work on the sphere given in Table 2 sheds some light on this discovery. Apparently, the coarser mesh resolution of 10.8 c/d triggers an onset of plastic deformation at a lower initial velocity. This can be explained by the larger cell sizes in the sphere and the well-known artificial increase of target strength due to a coarser mesh. The idea is here that the mass transport of larger cells towards the edge of the penetration channel dissipates more energy than that of smaller mass packages. Similarly, larger cells in the sphere require additional energy to be equally deformed. Moreover, Fig. 7 reveals that using a finer mesh with 18 c/d results only in a marginal change with respect to the resolution of 13.5 c/d. Consequently, for the sake of computational efficiency, the latter mesh resolution is chosen as the benchmark setting throughout this work.

In order to investigate how mesh resolution variations influence the simulated residual velocities in an entirely plastic regime of the sphere, simulations with higher initial velocities would be required. Since experiments with such higher initial velocities result in fragmentation of the sphere [22,25,26] a direct comparison to the purely elastic regime is quite difficult. The change in the penetration mechanism of the projectile introduces additional phenomenological aspects for the description of the material. However, the focus of this work clearly lies on modeling the ballistic performance of adobe. So we leave the issue of material failure of the projectile for future investigations and at this point only consider the velocity regime in which the projectile stays intact.

4.4 The role of pore compaction properties

![Fig. 8: Sensitivity analysis of pore compaction path parameters (see Eq. (1a,b)). Residual velocities as a function of initial velocities for perforation experiments and simulations with one adobe brick as a target. Left: Variation of the pore compaction pressure $P_{\text{comp}}$. Right: The compaction exponent $N$ is varied. The mesh resolution of the target is 13.5 cells per diameter of the sphere (c/d).]
With the yielding threshold of the projectile and the mesh convergence of the target investigated, we can now focus the study on the material parameters of the RHT concrete model used for adobe (see Table 1). As mentioned in subsection 2.2, the parameters of the pore compaction curve (Eq. (1b)), namely the solid compaction pressure $p_{\text{comp}}$ and the compaction exponent $N$, are obtained by an initial estimate and a subsequent sensitivity study. This includes a comparison of the simulated residual velocities to the ballistic data of Refs. [22, 25, 26]. Before that, we estimate the value of the initial compaction pressure with $p_{\text{el}} = 10 \text{ MPa}$. This value lies within the interval of $2/3 f_c$ and $12 f_c$ which constitutes the range found in literature [40, 41] for this parameter in concrete models. The solid compaction pressure $p_{\text{comp}}$ is then varied over an order of magnitude as depicted in the left panel of Fig. 8. It is immediately visible that a variation of this parameter has significant influence on the residual velocities. Hereby, a smaller value corresponds to a smaller area under the pore compaction curve (see Fig. 1), which is associated with the energy dissipated during the compaction process. Hence, this results in a higher residual velocity. For $p_{\text{comp}} = 1.0 \text{ GPa}$ there is a noticeable influence on the residual velocities with respect to benchmark simulations ($p_{\text{comp}} = 1.5 \text{ GPa}$) above an initial velocity of $1000 \text{ m/s}$. For lower initial velocities this decrease of the pore compaction path has no significant influence due to the lower pressures involved. Decreasing $p_{\text{comp}}$ by an order of magnitude with respect to the benchmark value leads to elevated residual velocities for all conducted simulations. Hereby, the deviation from the values obtained by the benchmark simulation decreases with increasing initial velocity.

By changing the compaction exponent $N$, the pore compaction path can also be varied. The higher the exponent, the smaller the area below the pore compaction curve will be (see Fig. 1) and hence the corresponding energy dissipation. The right panel of Fig. 8 shows that for initial velocities above $1000 \text{ m/s}$ the variation from $N = 3$ to values of 1 and 5 has a noticeable influence on the simulated residual velocities. Consequently, we can state that above a certain pressure involved in the impact event, the parameters describing the pore compaction process have a significant influence on the obtained residual velocities. Hence, it can be concluded that correctly including this process in the utilized material model is decisive for an appropriate description of high rate and shock loading of adobe.

4.5 Sensitivity of strain rate dependent strength

Fig. 9: Sensitivity study of dynamic strength parameters $\alpha$ and $\delta$ (see Eqs. (4a) and (5a)). Left: Residual velocities as a function of initial velocities for perforation experiments and simulations with one adobe brick as a target. The mesh resolution of the target is 13.5 cells per diameter of the sphere ($c/d$). Right: Rate enhancement (see $F_{\text{Rate}}$ in Eqs. (4a) and (5a)) as a function of strain rate for $\alpha$ and $\delta$ values used for the simulations in the left hand panel.
Fig. 9 displays a sensitivity study of the two parameters that describe the strain-rate-dependent strength as given in Eqs. (4a) and (5a). Therein the benchmark values for $\alpha$ and $\delta$ are exchanged by the smaller values of the material parameter set used for concrete for example in Ref. [8]. Additionally, significantly larger values of $\alpha = 0.2$ and $\delta = 0.1$ are tested. The simulated residual velocities exhibit a noticeable but moderate influence stemming from this variation of the strain rate exponents. Moreover, a tendency of a smaller influence on the residual velocities for larger initial velocities can be found in the left panel of Fig. 9. This can be understood from the growing importance of projectile deformation, pore compaction, and shock response for higher pressures in the impact event so that the relative influence of strength effects decreases. Considering the rather large variation of the strain rate exponents and the resulting large change of the compressive and tensile strengths (see right panel of Fig. 9), the influence on the residual velocities is rather small. Especially comparing the simulations employing the strain rate parameters of concrete in Ref. [8] (green diamonds) with the simulations using the Adobe1.8-RHT parameters (red circles) reveals a quite small change. Having in mind the discussion of the simulation results presented in Fig. 6 and Fig. 8, it becomes clear that for the given application, a proper onset of plasticity for the steel sphere and an appropriate description of the pore compaction path are more important than the details of strain-rate-dependent strength, especially at higher impact velocities. This finding is supported by conclusions presented in Ref. [27].

4.6 Deep penetration into layered adobe bricks

In order to widen the validation range of the previously presented material models and parameter sets on the description of bearing steel sphere projectiles impacting adobe bricks, we compare simulation results with ballistic penetration experiments employing semi-infinite targets. In the case of the ballistic experiments performed in Refs. [22, 25, 26] such a target consists of four 71 mm thick adobe bricks in series. In the simulation using a monolithic target, the thickness of these four bricks is retained while for the simulations utilizing separate bricks, two or three of them (depending on the initial velocity) separated by a gap of 0.5 mm are impacted. We believe that the latter target setup in the simulation is closest to the reality of the experiment, where putting two bricks with considerable surface-roughness together will certainly result in a partial reflection of stress waves at the interface of these two bricks. Hence this target setup is chosen as the benchmark in this work (also for the two brick targets in 4.1).
In Fig. 10, damage patterns of exemplary deep penetration simulations are displayed. On the left hand side, the damage pattern of a monolithic target and one consisting of confined separate bricks is shown next to each other. The difference in the onset of damage introduced by the existence of interfaces is clearly visible. For all multi brick targets there is a discontinuity of the damage propagation in between the bricks. In the direct comparison of a confined with an unconfined two brick target depicted on the right hand side of Fig. 10, it becomes clear that the confinement partially inhibits the growth of the damaged volume of the target. Confinement in this case means a boundary condition of vanishing velocities on the side, top and bottom surface. Since the material cannot expand beyond the defined target outlines in the latter case, there will not only be a higher ballistic resistance but also a higher pressure. Hence less damaged material with higher resistance is present because of the pressure dependence of both the strength surface and the equivalent plastic strain to failure in the damage evolution law (see Eq. (9)).

Simulated penetration depths in comparison to the corresponding values obtained from ballistic experiments [22, 25, 26] are depicted in Fig. 11. The simulated results of the benchmark settings (red full circles) are in acceptable agreement with the data (black full squares). However, we observe large variations comparing the penetration depths obtained with the benchmark settings to the ones with a different target setup (monolithic), mesh resolution (9 c/d), and added confinement through boundary restraints at the side, bottom and top surface. Interestingly, the change introduced by a much coarser mesh is in the same range as the variations stemming from confinement and switching from separate bricks to a monolithic target. For the former it is furthermore observed that close to an interface of two bricks (grey dashed lines), the dependence of the penetration depth on the mesh resolution vanishes for some points.

![Fig. 11: Penetration depths as a function of initial velocities for deep penetration experiments and simulations with a “semi-infinite” adobe target. The legend states which mesh resolution of the target, in cells per diameter of the sphere (c/d), and which setup and boundary conditions are used. The horizontal dashed lines indicate the positions of the interfaces in between the separate bricks (except for the monolithic target).](image-url)
In general it can further be stated that both target setup and confinement are much more important for deep penetration simulations than for the previously presented perforation simulations. For single brick targets, full lateral confinement showed no significant influence on the simulated residual velocity (see Table 3). Furthermore, the influence of switching to a monolithic target for the two brick perforation simulations results in a small difference at most (see values in Table 3). This can be explained in a rather simple and intuitive way. For the perforation simulations, there is always the free back surface which reduces the pressure build-up in the target. Confining the side, bottom, and top surfaces in the deep penetration simulation results in a practically fully confined volume with a significantly higher pressure level compared to the partially unconfined counterpart. This clarifies why there is a significant increase in penetration resistance and hence significantly lower penetration depths for the simulations with the confined target (open olive squares).

Other observations can be discussed by considering the influence of the target configuration and confinement on the propagation and reflection of stress waves and the thereby triggered onset of material failure. Due to the large initial velocities in the perforation simulations of targets with two adobe bricks, the sphere arrives at the back surface of the first brick just after the stress wave has been reflected. Thus, there is a small influence by pre-damage stemming from spall failure. Additionally, the sphere enters the second brick with a velocity still large enough to perforate it. In this case, the influence of the pre-damage of the front side of the second brick is small. Whenever the sphere leaves the back surface of a brick with a low velocity (<100 m/s) and impacts a nearly undamaged front side of the next brick, it is stopped almost immediately. Consequently, for a multi brick target there is a reduction of the penetration resistance at the back region of each brick while in the front region, an increased resistance to penetration is found with respect to a monolithic target. So the interfaces within the target significantly influence the penetration depths. The clustering of points around the dashed grey lines marking the target interfaces in Fig. 11 stems from this effect. For the simulations with the monolithic target (blue open triangles), this effect is obviously absent. Furthermore, the partially vanishing influence on penetration depths by mesh resolution is also tied to this interface effect. Since for low velocities close to a target interface, the discussed pre-damage of the material is decisive, effects stemming from the mesh resolution become secondary. Moreover, these low velocities result in a purely elastic behavior of the sphere which also makes the mesh resolution less pronounced and leads to faster convergence (see discussion of Fig. 7 in 4.3).

Lastly it should be noted that confined separated bricks and an unconfined monolith are ideal cases achievable only in a simulation. The reality of an experiment lies most probably in between both scenarios. Here the targets are embedded in a steel frame (see Fig. 3) with a limited form fit and consequently the bricks are not perfectly confined. Hence the unconfined target setup is chosen as the benchmark setting. This perception is corroborated by Fig. 11. However, both of the mentioned extreme cases can be used to estimate an upper and a lower limit for values of penetration depths of future experiments.

5. Conclusion and Outlook

From the good match of the results of simulations performed in this work with the existing ballistic data, we find that the chosen hydrocode modeling approach with the employed material descriptions and parameter sets are able to properly describe the impact of bearing steel sphere projectiles onto adobe brick targets. Hence the used strength and damage model with pressure, strain rate, and triaxial dependence in combination with a porous equation of state properly describe the key physical phenomena of the high-rate loading of adobe.
Hereby, model parameters are derived from material tests as far as possible and otherwise supplemented by engineering judgement. Most importantly, the failure surfaces stem from material tests with triaxial control and are thus highly trustworthy. Previously experimentally determined residual velocities and penetration depths are reproduced by the simulations with small deviations at most. Additionally, the damage patterns of the target as well as the deformation of the projectile are in good agreement with the data. The latter quantity is identified as an important fact because the onset of plastic deformation of the projectile is shown to be very important for testing the validity of the target description. The parameters defining the pore compaction path are found to be important since the energy dissipation associated with this mechanism must be included appropriately for a quantitatively correct description of the ballistic performance of the adobe target. On the other hand, a sensitivity analysis reveals an unexpectedly low importance of the strain rate dependence of the strength surfaces for the case investigated here.

Moreover, a mesh convergence study is performed with the perforation simulations, which reveals that resolutions below the chosen benchmark value are unable to capture the onset of plastic deformation of the projectile. In contrast to that, we discover almost no sensitivity on other simulation settings for perforation simulations when either comparing monolithic with multi brick targets or adding lateral confinement to the target bricks. For the deep penetration simulations, the situation is found to be different. Here, we reveal a much higher sensitivity to the chosen target setup and applied boundary conditions. Consequently, we conclude that simulations of the penetration of semi-infinite targets alone are not ideal for the validation of adobe material parameters. So this work suggests that perforation simulations are more suitable for this task since these are almost independent of such additional simulation settings. However, as it is usually the case in ballistic studies, the combination of both approaches leads to a wider validation range since they introduce complementary aspects to the investigation.

The successful reproduction of experimentally obtained values in this work suggests that the chosen material models and parameters will allow reliable predictions of similar loading cases of adobe targets. However, the possibility of a fortunate error cancellation within the employed theoretical description of the material must be excluded by further validation. Independently obtaining equation of state properties, especially along the compaction path, e.g. from flyer plate tests, can lead to increased confidence in this important model aspect.

A so far neglected characteristic of brickwork materials like adobe is that they can exhibit a large scatter range for many important material properties, like density, strength etc. Hence our theoretical description of impact experiments on adobe with the RHT concrete model and the Adobe1.8-RHT parameter set can be tested by a comparison to experimental results for adobe targets with different material characteristics. A successful prediction or reproduction of such data would be a convincing validation of a wider applicability of our here presented theoretical description of adobe.

Although it has yet to be confirmed in further studies, we perceive our theoretical investigations presented in this work already to be a first important step towards a predictive hydrocode modeling approach for impact and shock loading of adobe.

Acknowledgements
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References


Appendix

Table 1: Material parameters used for the hydrocode simulations. Parameters for Adobe1.8-RHT and for BearingSteel2.7YS are listed in the same manner as in the AUTODYN [31] material library. Selectable parameters are printed in cursive and have no units.

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<th>BearingSteel2.7YS</th>
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<td>7.75 (g/cm³)</td>
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<td>1.5 (+)</td>
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Table 2: Kinetic energies and plastic work of the projectile sphere from perforation simulations of one adobe brick with the material parameters of Adobe1.8-RHT (see Table 1). Cursive entries are changes with respect to the benchmark settings.

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Table 3: Influence of target setup, boundary conditions, and mesh grading on simulated residual velocities of perforation simulations (discussed in 4.6) together with the corresponding experimental values from Refs. [22,25,26]. The rows with the light grey background correspond to the simulations performed with the benchmark settings and parameters given in Table 1. Cursive entries are changes with respect to these benchmark settings. The mesh resolution is 13.5 cells per diameter of the sphere.

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Table 4: Influence of the erosion strain on simulated residual velocities of perforation simulations (discussed in 4.6) together with the corresponding experimental values from Refs. [22,25,26]. The rows with the light grey background correspond to the simulations performed with the benchmark settings and parameters given in Table 1. Cursive entries are changes with respect to these benchmark settings. The mesh resolution is 13.5 cells per diameter of the sphere.

<table>
<thead>
<tr>
<th>Initial velocity (m/s)</th>
<th>Residual velocity experiment (m/s)</th>
<th>Erosion strain</th>
<th>Residual velocity simulation (m/s)</th>
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